ANALYSIS OF LOSS OF COOLANT ACCIDENTS

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CERTIFICATE

This is to certify that this work on 'Analysis of Loss of Coolant Accidents' has been carried out under our supervision and has not been submitted elsewhere for a degree.

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CONTENTS

ABSTRACT

The present work investigates the two-phase critical flow rates in a loss of coolant accident of a PWR. An experimental loop was set-up and flow rates, static pressures, stagnation pressures and temperatures were measured. A new type of quick acting valve was developed, made of burnable mylar sheets, which gave quick, clean breaks. A new (indirect) technique to measure mass flow rate was developed by measuring changes in level. It was found that the technique needs refinement (by incorporating baffles, possibly) to cut out level changes due to vibration from the impact of the jet on the container. Unlike averaged measurement of flow this measures the flow continuously at all time instants after the break.

An integrated approach to the study of equilibrium critical mass flows by the Moody and Fauske methods has been presented. Both Moody's and Fauskes' models were found inadequate for low quality flows.

NOMENCLATURE

A	the cross sectional area of the break (m^2)
g_c	the gravitational conversion factor (Kgm/kgf)(ft sec^{-2})
G_c	the critical mass velocity (Kgm/ft^2-sec)
h_f	the enthalpy of saturated water (Kcal/kgm)
h_{fg}	the latent heat of vaporization (Kcal/Kgm)
h_o	the enthalpy of the steam-water mixture (Kcal/Kgm)
k	the slip ratio
P	the stagnation pressure (Kg/cm^2)
p	the static pressure (Kg/cm^2)
p_c	the critical pressure (Kg/cm^2)
S_f	the entropy of saturated water (Kcal/Kgm ^oK)
S_{fg}	$S_f - S_g$ (Kcal/Kgm ^oK)
S_g	the entropy of saturated steam (Kcal/Kgm ^oK)
S_o	the entropy of steam water mixture (Kcal/Kgm ^oK)
T	the thurst force (Kgf)
v_m	the specific volume of the steam water mixture (m^3/Kg)
x	the steam quality
W	the amount of the expelled water (Kgm).

CHAPTER I

INTRODUCTION AND GENERAL REVIEW OF THE PROBLEM

1.1 Introduction:

Energy crisis is one of the major problems the world is facing today. Many alternatives are being suggested to solve this crisis, like tapping Solar energy, more utilisation of hydro and nuclear power, etc. As far as solar energy is concerned, the commercialisation has yet to take place, and most of the hydro power has already been tapped. Then comes, nuclear power, which is expected to effectively solve the energy problems for many years to come. To cope with the demand, around 1000 nuclear power plants will have to be built by the year 2000. As the number of nuclear reactors will go up, there will be a strong need to study nuclear safety. The present work is an attempt to study an important aspect of nuclear reactor safety i.e. loss of coolant accident (LOCA) using an experimental loop.

1.2 Reactor Accidents and LOCA:

In this section we will consider reactor accident in general and LOCA in particular. Reactor accidents can occur in many ways. This may be due to control rod failure, loss of flow due to pump failure, failure of reactor safety mechanism, etc. These accidents can be divided into two

major categories, firstly the design based accident (DBA) which means that, kind of the event and severity are known prior to the accident, for example, loss of flow accident due to pump failure etc. For such accident safety measures could be taken during the design of the nuclear power plant. The second category of accidents are all those events which cannot be predicted before the operation of the plant, e.g. refuelling accident, spent fuel handling accidents etc.

Major consequence in all nuclear reactor accident is the release of large amount of radioactivity to the atmosphere due to the melting of the fuel in the reactor core. For a core melt-down i.e. for maximum credible accident, either cooling system should fail or the heat imbalance should allow the fuel to reach a temperature of 5000^{o}F. This would require the reactor shut-down.

A reactor safety study analysis[1] has defined these two situations as 1. Loss of coolant Accidents and 2. Reactor Transients.

1.21 <u>Reactor Transients</u>:

The term reactor transient refers to a number of events that require the reactor to be shut-down. These range from normal shut-down for refuelling etc. to unplanned but expected events as loss of power to the plant from

utility transmission lines. The reactor is designed to cope with unplanned transients, but if all of them fail together, then the heat would be sufficient to eventually boil away all the cooling water and melt the core.

In addition to the above pathway to core melt, it is also possible to postulate the core melt resulting from the failure of reactor shut down systems following a transient event . In this case it would be possible that available cooling systems are unable to cope up and a core melt-down would result.

1.22 Loss of Coolant Accidents (LOCA):

The loss of coolant accidents[2] are postulated to result from failure in the normal core cooling system. The water is at very high pressure in the reactor cooling systems and if a rupture occurs, in the pipes, pumps, valves or vessel, then a blow out would happen. In this case some of the water would flash to steam and blow out of the opening. The steam water mixture flows out of rupture until the pressure in the reactor and in the containment building become equal. This is known as blow down phase of a LOCA . For the first few seconds following the rupture, the fuel and cladding temperatures remain roughly constant, but with the onset of boiling about 5 sec. after the break, the temperature rises rapidly. For complex reasons,

having to do with the flow of water-steam mixture out of the vessel, the cladding temperature, after reaching a peak value of nearly 2000^{o}F, drops down again and then undergoes a steady rise until ECCS (Emergency Core Cooling System) intervenes. As soon as boiling occurs in the core, the reactor falls subcritical because of the negative void coefficient.

For most accidents, i.e. core melt-down, LOCA must be followed by failures in emergency core cooling systems. But the principal exception to it is massive failure of the large pressure vessel. However, the accumulated experience with pressure vessels indicate that the probability of such accidents is very small and hence it does not pose much safety problems.

1.3 Literature Survey:

Inspite of the great deal of work done on LOCA, the question of nuclear reactor safety has not yet received a satisfactory answer. One of the earliest reports in this direction was a study entitled, 'Theoretical possibilities and consequences of major accidents in large Nuclear Power Reactors carried out by Brookhaven National Laboratory in 1957. Investigations into LOCA were also started around the same time.

Since the coolant in a majority of present day reactors is water, research in two phase flow has contributed a great deal both in terms of theory and experiments. The theory has found application for nonaqueous coolants as well. The first full scale experimental facility LOFT (Loss of fluid facility) was set up in Nov. 1964 by Idaho Nuclear Corporation under the U.S.A.E.C. The other two set-ups were the ROSA-1 in Japan and DHT-1 by EURATOM at Ispra.

1.31 Theoretical Basis:

The final aim of all LOCA experiments is the prediction of the thermohydraulic history of the core. This entails the measurement of G_C the maximum critical flow rate at the ruptured section, which will determine, along with initial conditions, the thermal hydraulic history in question.

Several flow models have been proposed to determine, G_C. They can be broadly classified into two categories (i) Non equilibrium models (2) Equilibrium models, while equilibrium models have been widely studied, the non-equilibrium models have yet to establish themselves. Below we give a brief account of non equilibrium models and a more detailed description of equilibrium models.

The non-equilibrium theories assume , the possibility of thermal non-equilibrium[3] between phases due to finite evaporation rates. Henry and Fauske treated non-equilibrium by imposing a condition of zero quality change between stagnation and throat. The vapour is assumed to expand isentropically and independently of the liquid fraction. The tendency of this limiting theory is to over-estimate the nozzle flow rate. In general non-equilibrium fit better for short pipes, orifices and for stagnation qualities above 2%.

The equilibrium theories, assume a thermal equilibrium between the phases at the time of rupture. The first attempt in this direction was made by Isbin et al[4] assuming a homogeneous flow model with the fluid properties averaged out. This model gave good results for large breaks, but for intermediate and small breaks, this model was inadequate. Fauske, H.K.[5], after a great amount of experimental work, assumed that the two phase flow at the time of blow-down was annular with liquid in the annulus and vapour in the central portion of the tube. The model gave good results for all breaks but only for low pressures (0 - 360 psi). In his model, Fauske used continuity and momentum equations. Moody[6] proposed another mathematical model to calculate critical mass flow rate by using continuity and energy equations. A detailed account of these two models is given in

Chapter three. Many analytical models were proposed soon after this work. Margolis, S.G., and Redfield[7] devised an analytical model for blow-down experiments. This model was found inaccurate for describing experimental measurements on the blow-down of simple tanks. They also studied[8] reactor plant dynamics during LOCA. By assuming that the primary system was divided into three parts, rather than one lumped volume i.e. (i) pressuriser, (ii) primary coolant from the pump exit to the core inlet, he claimed that he got better results.

1.32 Experimental Methods:

The three full scale experimental facilities, as mentioned earlier, will be described first. Then we go over to other experimental works and application of computer codes in the study of LOCA.

(a) LOFT. The loss of fluid integral test was designed by Coplen and Ybarondo[9] to study all the effects that were anticipated in a LOCA. The intention was to combine the possibility of studying nuclear, thermal hydraulic and structural processes occurring during a LOCA. Curet,[10] performed blow down tests to determine the influence of reactor vessel internal flow restrictions on the fluid decompression behaviour during a LOCA. Several different geometries were used to isolate the effects of hydraulic

resistance and fluid inertia on the nature of i) core pressure drops occuring during both the subcooled and saturated two phase blow-down ii)fluid flow separation, iii) mass discharge from the system. Tests were performed with a hydraulically simulated scaled LOFT core for breaks of various sizes in both the upper and lower plenums and it was found that the internal flow restrictions increase fluid phase separation and consequently increase residual water in the vessel after blow-down. The residual water was important as a source of steam for both core-cooling and metal-water reactions as well as for the calculation of core-flooding flow rates for ECCS. Using the same, Berta[11] studied subcooled pressure gradients in PWR systems following loss of coolant. He used an approximation-method to determine the magnitude and duration of the pressure gradient. This method was an empirical formulation involving generalised system component dimensions and fluid properties. The results were compared with values obtained from the LOFT semi-scale blow-down test programme. The method consisted of evaluating a series of equations for each of the two plenums. The solutions of the series of equations for the plenum closest to the pipe break gave the pressure relative to the initial pressure in successive time steps. The time steps were computed by dividing the length of pipe between the break and vessel by the velocity of sound in the

pipe. The same kind of information was provided for the other plenum in combination of two time steps, one being the previously defined one and the other calculated by dividing the core-length by the sound velocity in the core. Pressure gradients across the core are determined by differencing the pressure in the two plenum immediately above and below the core.

Agreement between results obtained from the above calculation and LOFT data was good. The author claimed that the method was applicable to a wide value range of parameters which include the dimensions of existing and proposed PWR's.

A.N. Nahavandi et al.[12] did a space dependent LOCA analysis for PWR's. The mathematical model used in the analysis was a very general and flexible one applicable to a large number of systems. The model allowed single, two phase and super-heat flow regimes & accommodated flow reversal between adjacent elements and choking flow at rupture points.

The analysis employed a 3 dimensional approach (length, radius and time) in the reactor core, a two dimensional approach (length and time) in the reactor coolant loops and a lumped parameter representation in the secondary steam generating system. The fundamental equations of mass and energy for each element and momentum equation along axial directions were expressed and then these equations were

combined with 1) energy balance equations and heat transfer correlations for the fuel rods and steam generator heat exchange processes, 2) subcooled, two phase and superheat state property correlations and 3) critical flow equations at the rupture elements. The resulting system of equations was solved numerically (SATAN Code) and the results were verified experimentally at loss of fluid test assembly (LOFT). From this computer program the effects of hot leg and cold leg ruptures in pressurised water reactors, were determined. Pressure, temperatures and flow rates were computed as functions of time at various locations in the reactor vessel with broken and unbroken loops. The LOCA was thus analysed in more detail. Calculations showed fair agreement with LOFT tests.

b) ROSA-I Assembly:

The ROSA-I assembly was designed at Japan Atomic Energy Research Institute, its purpose was to study the thermo-hydraulic behaviour within the primary coolant system during a LOCA. Makota Sabajima[13] analysed LOCA with the help of the above assembly. In the ROSA-I assembly, the experiments were carried out by discharging water at high pressure and temperature corresponding to reactor conditions from a pressure vessel, which was isolated from the heat up systems. Comparison of the ROSA-I results with RELAP-3, (a code used for the analysis of LOCA) revealed that results do not agree well under

certain break conditions, i.e. for hot leg rupture, the pressure and liquid mass calculated did not agree well with the experimental data.

The calculated pressure was lower than the data in the initial part of the transient and higher in the latter. Finally it was determined that (i) the discharge coefficient used in the code as a parameter could be correlated with the quality of the discharged fluid and (ii) the calculated liquid mass transient did not always agree with the experimental one when constant critical mass velocity was assumed. This discrepancy arose from the model dealing with the LOCA phenomena.

c) DHT-1 Loop:

The DHT-1 loop (depressurization and heating transients) was built at the joint Research Centre Ispra, within the reactor safety program of EURATOM. The objective was to study transient heat transfer phenomena during depressurisation under PWR conditions. The special loop DHT-1 were essentially one sub-channel and the upper and lower plenum of a PWR.

Friz and Riebold[14], with the help of this loop carried out a comparison between code calculations and blowdown experiments simulating a LOCA in PWR. The test program was developed by GAAA (Groupement Atomique Alsasienne Atlantique) at the same time but independently. This program

calculated the thermohydraulic history of the coolant within the core after a rupture in the primary circuit. The code calculated at different axial and radial positions within the core, the evolution of the coolant conditions and the temperature distribution within the rods and cladding of the fuel elements.

A series of experiments were carried out on DHT-1 simulating rupture in the hot leg of a primary coolant circuit In the loop only three important components i.e. test section, upper plenum and lower plenum simulated by two additional volumes were present. The loop did not contain pump or heat exchanger. To ensure defined flow conditions prior to rupture, the whole loop was heated by trace heaters to induce natural connection upwards in the test section. The measured parameters were pressure and temperature of the test section, ~~which are~~ recorded on a multichannel recorder.

The agreement between experimental and code calculations was found satisfactory, specially for large rupture cross-sections. It was found that the experimental results agreed with calculations made on the basis of following assumptions.

(a) Moody's correlation with a contraction coefficient of 0.7.

(b) Homogeneous flow model (i.e. slip ratio = 1).

At the General Electric Company, a pipe rupture study was done for the reactor primary circuit[15]. It was designed to study the mechanism causing failure and to provide improved capabilities for evaluating reactor piping systems to minimise the probability of failure. They made a) fatigue studies through bunch scale tests b) a failure probability study of actual reactor piping systems. Ishikawa, M. et al.[16] at JAERI have analysed blow-down behaviour of JPDR II (90 MW(t) BWR), using the RELAP-3 code[17]. A series of tests were made on the number of nodes [illegible] to obtain meaningful data. G. Gaspari et al.[18] compared the scaled down experimental results with that from analytic models and codes to predict the thermo-hydraulic history. The experimental measurements were carried out on a scaled down blow-down facility in terms of pressure, heated channel mass hold up and time to dryout under transient conditions determined by flow stoppage, break at different positions & power excursions. A blow-down code TILT was developed which has proved to be a useful tool for predicting the thermo-hydraulic history of the core.

Esposito[19] studied the mathematical aspects of reactor blow-down and made a detailed analysis. To simulate LOCA, a large number of equations describing various thermo-hydraulic phenomena must be solved. Below a review is presented

of some of the existing computational methods used for this simulation. Appett,K.D. et.al.[20] investigated the pressure oscillation that occurred during blow-down in the pressure suppression system. It was carried out in the Marviken Nuclear Power Plant Sweden, from Aug. 1972 to May 1973. The object of the investigation was to determine the pressure oscillations occurring on the floor of the condensation chamber with blow-down, also to determine the accompanying mass flow composition of the mixture. Steinkamp.).[21] studied the analysis of the thermohydraulic behaviour of PWR plants in case of minor breaks in primary coolant systems. It was necessary to apply different approaches in this case, since the ruptured surface was so small that at no time during the course of the accident the core was completely exposed to the coolant. There was no refilling and rebonding phase as in case of large break LOCA. It was found that Fauske's model holds good in this case. Jenson et.al.[22] carried out blow-down heat transfer tests at Idaho Nuclear Corporation. The experimental data were presented in this report. The test was conducted at 542°F and 2260 psia (Reactor conditions), and a double ended cold leg rupture was investigated. The electrically heated core was used in the pressure vessel to simulate the effects of a nuclear core. The flow resistance of the intact loop was based on core area scaling. During system depressurisation

core power was reduced from the initial level of 1.2 MW, in such a manner to simulate the surface heat flux of LOFT nuclear fuel rods until such time that departure from nuclear boiling occurs. Blow-down to pressure suppression was accomplished without ECC injection. The data was found useful.

1.4 Scope of the Present Work:

The work done basically constitutes the first phase of an investigation into the loss of coolant accident. We have experimentally determined the two phase critical mass flow rate (Gc) of the coolant and compared it with the theoretical values obtained using different flow models. Chapter two discusses, the two flow models used to predict maximum two phase flow rates, which form the theoretical basis of the problem. Chapter three contains an account of the experimental work performed during the course of this study. In Chapter four we give the experimental results, and their interpretation. It also includes a few suggestions for further work.

CHAPTER II

THEORETICAL ANALYSIS

2.1 Introduction:

The general description of the loss of coolant accident and the need for analysis of such an accident in a nuclear power plant have been discussed in Chapter One. The present work simulates the loss of coolant accident in a PWR by causing a rupture in the pipe line. Due to the rupture in the pipe line the steam water mixture escapes and the behaviour of the escaping fluid is analyzed by considering flow through an ideal nozzle. The purpose of this chapter is to study the two phase flow in this ideal nozzle.

The maximum flow rate of a single component, two phase mixture is of great practical importance because it restricts the capacity of the two phase flow rate. A number of models for obtaining the maximum flow rate of a two phase mixture have been proposed. Among these are the works of Linning[23], Isbin et al.[4], Fauske[5] and Moody[6]. Of which the last two have been found suitable for blow-down experiments. The Linning and Isbin models were based on empirical calculations. Moreover they assumed homogeneous flow model i.e. liquid and vapour are in equilibrium in the flow path (i.e. in the nozzle section) and the flow properties are

average of two phases. This approximation does not give good results for blow-down experiment. Moody's and Fauske's models assume separated flow and they have been found suitable under different conditions. The present work is based on these two models. We will first discuss briefly the homogeneous flow model in Section 2.2 and then go over to a detailed discussion of the separated flow models of Fauske and Moody.

2.2 Homogeneous Flow Model:

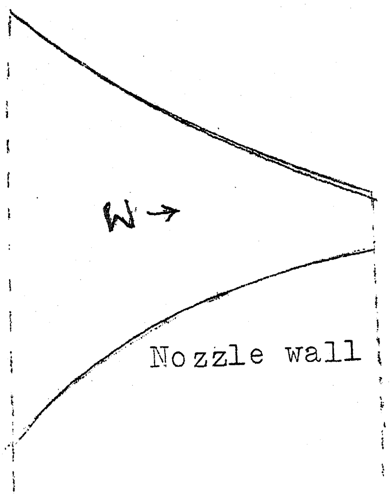
In homogeneous flow model it is assumed that a steam-water homogeneous mixture is flowing at a uniform velocity with the phases in equilibrium (No slip flow). The flow is steady and isentropic. The critical flow condition is defined under the restraints that the change in mass flow rate with respect to pressure is zero at constant entropy i.e.,

$$\left(\frac{dG}{dP}\right)_S = 0$$

Maximum single phase, homogeneous flow rate :- Homogeneous flow properties entering and leaving an ideal nozzle (entrance velocity is zero, total energy flow rates at entrance and exit are equal) are shown in Figure 2.1.

Properties at Entrance

$G = 0$
h_o
P
s_o
$U = 0$



Properties at Exit

A
G
p
h
u
x
v
U
X
$s - s_o$

Fig. 2.1: Ideal nozzle and homogeneous flow properties.

Continuity and energy equations are given by,

$$G = \frac{u}{V} \tag{2.1}$$

$$h_o = h + \frac{u^2}{2g_c J} \tag{2.2}$$

where,

G = Mass flow rate/unit area, lbm/sec-sq.ft.

h = Specific enthalpy, Btu/lbm

u = Velocity, fps,

V = Volume, cubic ft.

Subscript o refers to stagnation property

For isentropic flow, h and V are functions of p only:

$$h = h(s_o, p) \tag{2.3}$$

$$V = V(s_o, p) \tag{2.4}$$

where,

s_o = Specific entropy Kcal/Kg. °K

p = Static pressure Kg/cm^2

Solving for G from (2.1) and (2.2) we have,

$$G^2 = 2g_c \, J \left(\frac{h_o - h}{V^2}\right) \tag{2.5}$$

Equations (2.3), (2.4), (2.5) show that G is a function of P alone when h_o and s_o are known. A maximum value of G then must satisfy,

$$\frac{dG}{dp} = 0 \tag{2.6}$$

which leads to,

$$G_M = -g_c \, J \, \frac{1}{V} \left(\frac{\partial h}{\partial V}\right)_{s_o} \tag{2.7}$$

From the thermodynamical consideration,

$$Tds = dh - \frac{Vdp}{J'}$$

where,

T = Temperature, degree R

J' = $J/144 = 5.403$ ft^3-lbf/in^2-Btu

if $ds = 0$ for isentropic process, it follows that

$$\left(\frac{\partial h}{\partial V}\right)_{s_o} = \frac{V}{J'} \left(\frac{\partial p}{\partial V}\right)_{s_o} \tag{2.7a}$$

Now (2.7) becomes,

$$G_M = - g_c \, \frac{J}{J'} \left(\frac{\partial p}{\partial V}\right)_{s_o}$$

which is the expression for critical mass flow of a single phase homogeneous fluid.

2.3 Separated Flow Models:

Both Fauske and Moody developed expressions for the critical mass flow rate of a two phase mixture assuming the vapour and liquid are separated in the nozzle with steam flowing in the outer region and the liquid flowing in the inner region. The other assumptions on which the models are based are:

i) isentropic flow,

ii) the liquid and vapour are in equilibrium throughout the flow path,

iii) average velocities of the two phases are different (i.e. slip flow is considered)

iv) Flow is steady.

Fauske's model is based on the continuity and momentum equations while Moody has developed a model which maximizes the theoretical two phase flow rate based on continuity and the energy equations. Different slip ratios are obtained in their models and appears that this discrepancy is due to the use of momentum equation in one model and the energy equation in the other. Note that, although the starting equations are different in the two models, the basic mathematical considerations to develop the critical mass flow equations are the same for both the cases. This point was unnoticed sofar and both the models will be described here together to understand this in a better fashion.

The continuity equations for vapour and liquid in a two phase flow are,

$$G\,x = \alpha\, u_g\, \rho_g \tag{2.8}$$

$$G(1-x) = (1-\alpha)\, U_f\, \rho_f \tag{2.9}$$

where,

G = mass flow rate per unit area of the homogeneous mixture abbreviated 'mass flow rate' lbm/sec-ft^2

U = Average velocity ft/sec.

ρ_m = density lbm/ft^3

α = Vapour volume fraction

x = Quality of the mixture

Subscript g refers to vapour and f refers to liquid.

Note that the left hand side of equation (2.8) and (2.9) describes the property of the mixture at the exit of the nozzle.

In the absence of friction, the momentum equation for such an isentropic two phase flow can be written as,

$$\frac{1}{g_c}\,\frac{d}{dz}\,(1-\alpha)\,\frac{u_f^2}{V_f} + \alpha\,\frac{u_g^2}{V_g} + \frac{dp}{dz} = 0$$

i.e. $$\frac{dp}{dz} + \frac{G^2}{g_c}\,\frac{d}{dz}\,(1/\rho_m) = 0 \tag{2.10}$$

where, p = static pressure lb$_f$/ft^2

$1/\rho_m$ = mean specific volume

$$= \frac{x^2}{\alpha} V_g + \frac{(1-x)^2}{(1-\alpha)} V_f \qquad (2.11)$$

Now the slip ratio K is defined as,

$$K = \frac{u_g}{u_f} \qquad (2.12)$$

From (2.8) and (2.9),

$$K = \frac{x}{1-x} \frac{1-\alpha}{\alpha} \frac{V_g}{V_f} \qquad (2.13)$$

Eliminating α between (2.11) and (2.13),

$$1/\rho_m\ (x,\ k,\ p) = -g_c/[\ \frac{d(1/\rho_m)}{dp}\ ]_s \qquad (2.14)$$

From Eqn.(2.10) $\quad G^2_{Fauske}\ (x,K,P) = -\ g_c/[\,d\ \frac{(1/\rho_m)}{dp}]_s \qquad (2.15)$

Since Moody's model is based on the energy equation (and not the momentum equation), some additional properties of the flow have to be specified. These are v) entrance velocities are zero, vi) total energy flow rates at entrance and exit are equal. With these assumptions the energy equation takes the form,

$$h_o = x(h_g + \frac{u_g^2}{2g_c J}) + (1-x)\ (h_f + \frac{u_f^2}{2g_c J}) \qquad (2.16)$$

where,

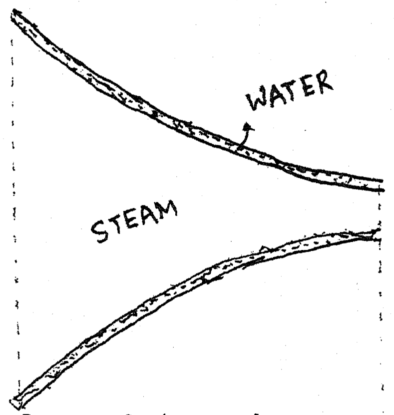
h = specific enthalpy Btu/lbm

Subscript o refers to property at stagnation state

J = mechanical equivalent of heat $\approx$ 778 ft-lb_f/Btu.

Properties at Entrance

$G = 0$
h_o
P
s_o
$U_f = 0$
$U_g = 0$



Properties at exit

G
h_o, h_f, h_g
K
p
$U_f,\ U_g$
V_f
V_g
x
α

Fig. 2.2: Ideal nozzle and two-phase annular flow properties.

From eqn. (2.13) we can write α as,

$$\alpha = \frac{1}{1+K\left(\frac{1-x}{x}\right)\frac{V_f}{V_g}} \tag{2.17}$$

where, $V = 1/\rho$ = specific volume.

Properties ~~a~~along the saturation line are functions of pressure only,

$$\begin{aligned} V_f &= V_f(p) & s_{fg} &= s_{fg}(p) \\ h_f &= h_f(p) & V_g &= V_g(p) \\ s_f &= s_f(p) & h_g &= h_g(p) \\ h_{fg} &= h_{fg}(p) & s_g &= s_g(p) \end{aligned} \tag{2.18}$$

s_o and s may be given in terms of p and x

$$s_o = s(p) = s_f(p) + x\, s_{fg}(p) \tag{2.19}$$

Finally equations (2.8),(2.9) and (2.16) through (2.19) may be combined to give the following relationship for G:

$$G_M^2\,(x,\ K,p) = \frac{2g_c\ J\,[h_o - h_f - \frac{h_{fg}}{s_{fg}}(s_o - s_f)]}{\left[\frac{K(s_g - s_o)\ V_f}{s_{fg}} + \frac{(s_o - s_f)}{s_{fg}}\right]^2 \left[\frac{s_o - s_f}{s_{fg}} + \frac{s_g - s_o}{K^2\ s_{fg}}\right]} \tag{2.20}$$

where M refers to Moody's model.

If the stagnation entropy s_o is known, then from equation (2.19) it is obvious that G (both in Fauske's and Moody's models) becomes a function of K and p only. Thus $G = G(K,p)$. Therefore, if G has a maximum, it must satisfy the necessary conditions,

$$\left(\frac{\partial G}{\partial K}\right)_p = 0 \tag{2.21}$$

$$\left(\frac{\partial G}{\partial p}\right)_K = 0 \tag{2.22}$$

The condition (2.21) gives the value of K at critical flow. From equation (2.20) and using (2.21) we get,

$$K_M = \left(\frac{V_g}{V_f}\right)^{1/3} \tag{2.23}$$

where K_M = slip ratio at critical flow for Moody's model.

In Fauske's model, we have to use a condition which is equivalent to equation (2.21) to find out the slip ratio namely,

$$\frac{d(1/\rho_m)}{dK} = 0 \qquad . \quad (2.24)$$

Fauske, in his model, assumed that at critical flow the pressure gradient $\frac{dp}{dz}$ attains a finite maximum value. Now, differentiating (2.10) with respect to K

$$\frac{d}{dK}\left(\frac{dp}{dz}\right) + \frac{G}{g_c}\frac{dG}{dK}\frac{d}{dz}(1/\rho_m) + \frac{G^2}{g_c}\frac{d}{dK}\frac{d}{dz}(1/\rho_m) = 0$$

Using Fauske's assumption and equation (2.21) we get

$$\frac{d}{dz}\;\frac{d}{dK}(1/\rho_m) = 0$$

i.e. $\frac{d}{dK}(1/\rho_m) = \text{Const.}$

Fauske assumed this constant to be zero and thus employed equation (2.24) to calculate the slip ratio for critical flow.

Thus from (2.24) and (2.14), we get,

$$K_F = \left(\frac{V_g}{V_f}\right)^{1/2} \qquad (2.25)$$

where K_F = slip ratio at critical flow for Fauske's model.

Now, using eqn. (2.25) in (2.15) and eqn. (2.23) in eqn. (2.20), G becomes a function of p only. To get the critical flow rate, we now use the condition (2.22). For this a numerical technique is employed and the prescription is as follows:

Assume a value of quality x, and then calculate G for various p, the point of inflation is noted. The quality is changed, and the calculation of G is repeated for various p to get another point of inflation. Thus the maximum flow rate is obtained.

We will describe the indirect experimental procedure to measure quality of the two phase mixture.

2.4 Determination of Quality:

As we have seen that earlier methods can predict the critical flow rate for steam water mixtures from known static pressure, enthalpy and quality conditions, but these methods cannot be applied to the vessel blow-down calculations because the quality of steam water mixture cannot be experimentally measured.

The present work is based on an approach due to . Shiba and . Curet[24] to determine the quality of the mixture using pressure data available from experimental and two phase flow models. With the quality information, the critical mass velocity can be calculated and integrated through the process of the blow-down and multiplied by the cross sectional area of the flow path to determine the amount of expelled water. The accuracy of the critical mass velocity calculations can be determined from the

comparison of the calculated and measured values of expelled water, during blow-down tests.

Thrust and Critical Flow Relationship:

From the earlier developments, we see that when appropriate value of K is substituted in equation for G, G becomes a function of p and x in both Fauske's and Moody's models,

$$G = G(p,\ x) \tag{2.26}$$

From the simulated experimental assembly we get the stagnation pressure within the vessel, during blow down. The experimental pressure and thrust data in conjunction with analytical techniques can be used to determine the quality required to predict the critical mass velocity for vessel blow down. The thrust applied to the cross-section of flow path is,

$$T = P \times A \tag{2.27}$$

where P is the stagnation pressure, and A is the cross-sectional area of the break.

Now, from a simple force balance, the thrust is the summation of the momentum expulsion rate and the pressure force,

$$\frac{T}{A} = p + \frac{G^2}{\rho_m g} \tag{2.28}$$

where p is the static pressure, and ρ_m (the mean density) is,

$$\frac{1}{\rho_m} = [x\, v_g + (1-x)\, K\, v_f]\, [x + \frac{1-x}{K}] \tag{2.14}$$

From equation (2.28), (2.14) and equation (2.27), one can easily obtain G as a function of x and p. Now, from this equation of G and equation (2.26), (Moody's Fauske's G), x can be eliminated and G becomes a function of p only. Then the application of the condition (2.22) becomes straightforward. But the elimination of x may become cumbersome and hence we tried to get rid of this by suitably using experimental results in eq. (2.28). From the experimental data, amount of water expelled is known for a given period of time if the differential pressure between the system pressure and outside pressure is assumed to be a constant.

$$W = A \int_0^t G\, dt = A \quad G\, t \tag{2.29}$$

The critical mass flow is obtained from the corresponding maximum amount of water expelled.

$$W_{max} \approx A \quad G_c \quad t \tag{2.30}$$

Then equation (2.22) for critical flow becomes,

$$\frac{T}{A} = p_c + \frac{G_c^{\,2}}{\rho_m g} \tag{2.31}$$

where,

p_c = static pressure corresponding to critical flow (experimentally measured).

G_c = critical mass flow rate

Note that, ρ_m is a function of pressure. Putting the value of G_c from (2.30), in (2.31) ρ_m is calculated. Then from (2.14), the quality x is known. Once the quality is thus experimentally determined, it can be substituted either in Moody's or Fauske's equation (2.26). Then by virtue of (2.22) the theoretical G_c can be calculated. This theoretical value can be compared with experimental G_c. But this procedure has a big drawback in the assumption that the differential pressure between system pressure and outside pressure will remain constant.

This driving differential pressure is very important[27] because it is used to overcome the acceleration, frictional and other pressure losses of the escaping fluid up to the point of break. In a system, where the volumes inside and outside are very large compared to the size of break, the differential pressure can be assumed constant. But the experimental assembly, which Curet has used, the pressure drops to its 50% value with in one second, this assumption is not valid. So the critical mass velocity, which we will

get by this model, will just be an average escape velocity. We have tried to modify this system, by assuming the escape velocity constant for a fraction of second and then tried to use the same analysis. Moreover, we are indirectly measuring the critical mass velocity continuously and recording it on the storage oscilloscope. We feel that this assumption will hold good for calculating the critical mass velocity in our approach.

CHAPTER 3

EXPERIMENTAL ANALYSIS

3.1 Introduction:

Many experimental assemblies have been built to study the loss of coolant accident. The present experimental work has been performed to check the critical flow rate models i.e. choked flow models. The prediction of critical mass flow rate is important for analysing reactor safety because this determines the maximum escape rate of a high pressure fluid from the system in which it is contained, which in turn fixes the pressure, that the containment has to with stand.

3.2 Experimental Assembly:

The schematic diagram of the assembly is shown in figure 3.1. It consists of a test section, with upper plenum, lower plenum, a water pump, tanks to collect the fluid, quick acting valves and a flowmeter. Each of these will now be described in detail.

(a) Test Section:

The test section consists of a mild steel pipe, of 1 in. internal diameter, $1^1/8$ inch. outer diameter. It has five

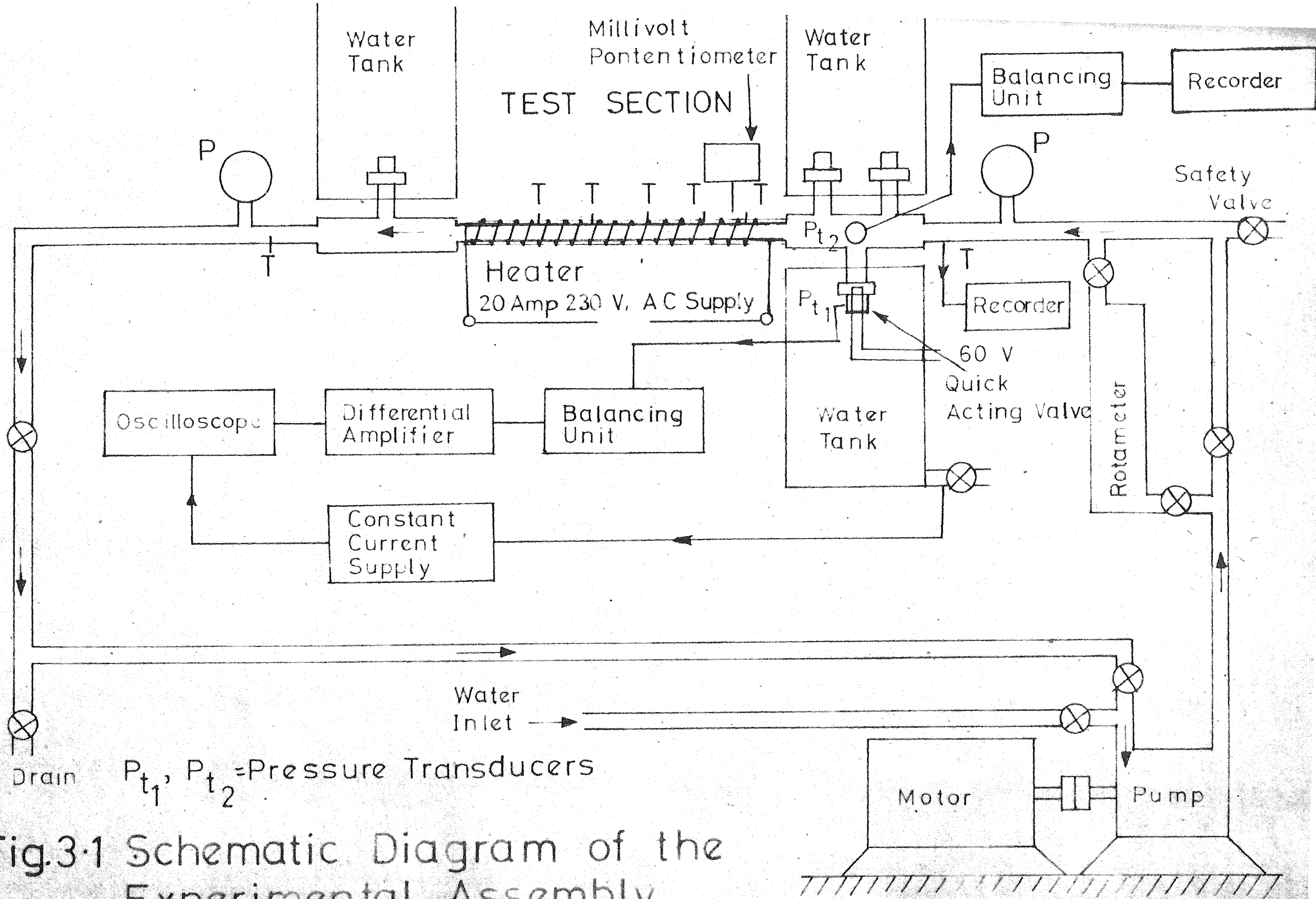


Fig.3·1 Schematic Diagram of the Experimental Assembly

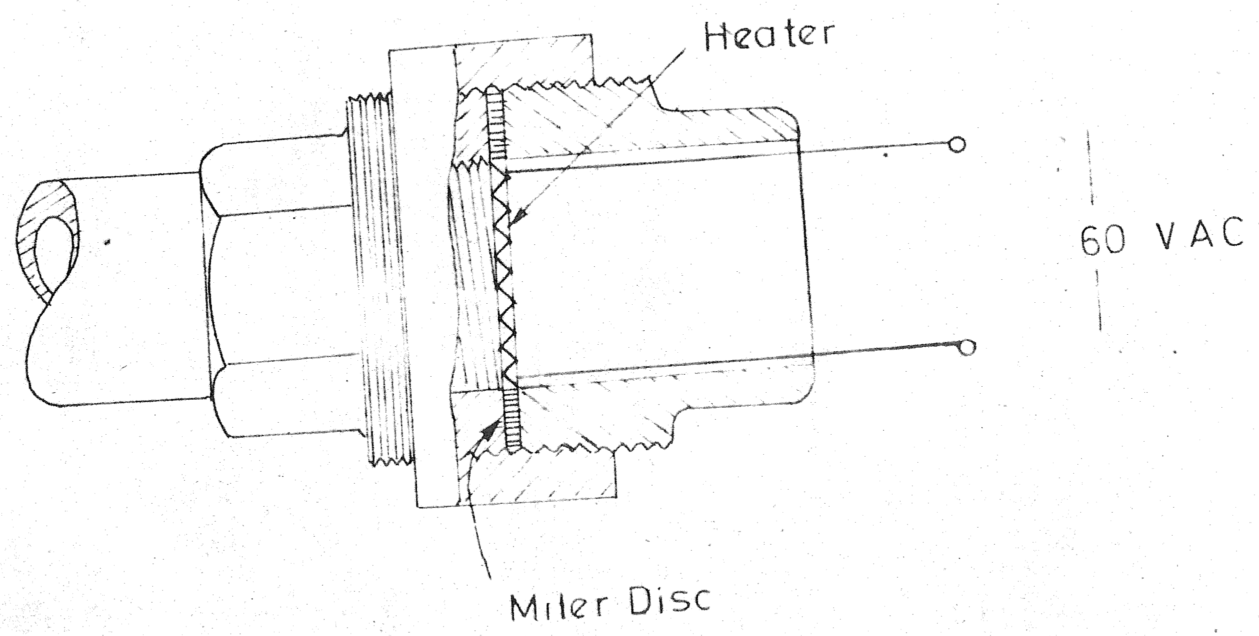


Fig. 3·2 Quick Acting Valve

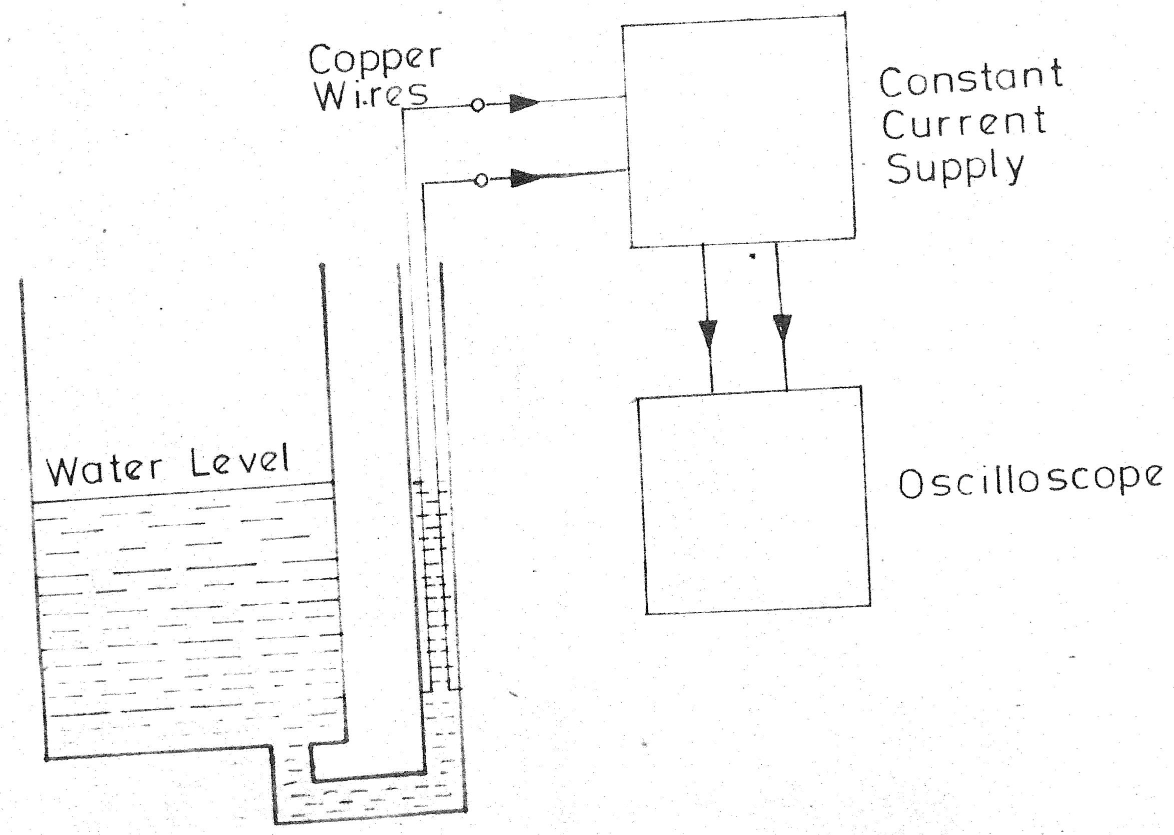


Fig.3·3 Transient Water Level Measurement

openings for fixing thermocouples. made of copper-constantan. The test section is heated externally by a heater which was fabricated in our laboratory. Originally, we had planned to simulate the heat flux of a 220 MWe PWR reactor in our set-up, but due to experimental limitations, this could not be achieved. A heater of 3.2 Kilowatt was fabricated with a 20 SWG twisted (double) nichrome wire. Initially, a double layer heater was tried, we tried to insulate the layers by glass - fibre, and then by thick mica sheets, but due to frequent short circuits, this idea was given up. The nichrome wire was wrapped on the test section. The major difficulty in wrapping of the resistance wire was the separation between consecutive wires, as less than adequate separation resulted in a short circuit. By trial and error method we found that the separation between two wires should not be less than 3 mm. Also, a mixture of white cement and asbestos powder was filled in the space between the wires. Subsequently, thermal insulation was provided by pasting asbestos and white cement mixture around the heater. The higher heat flux can be achieved by the use of an induction heater.

(b) Upper and Lower Plenum:

The upper and lower plenums of the reactor were simulated by using two 3" inner diameter mild steel pipes,

which had openings for quick acting valves and couplings for pressure transducers. The plenums were fabricated in our central workshop. The holes were drilled on the plenum surface, where counter part of quick acting valves were welded. The water expelled was collected in aluminium tanks of dimensions $1' \times 1\frac{1}{2}' \times 1\frac{1}{4}'$. These tanks were fabricated in the central workshop. The lower plenum has two tanks, one for double ended break and another for single ended rupture. The upper one has only one tank for single ended rupture.

(c) Water Supply System:

The water was stored in a tank $6' \times 4' \times 4'$ from which a four stage, high pressure, centrifugal pump forced the water to the test section. Specifications of the pump and motor are given below.

Pump: BE - S141 Model A 02140K

Capacity 3 gpm at 300 psi (maximum)

Motor Crompton Parkinston, Bombay

1430 RPM, 4 Phase 440 V

7.5 A, Mesh, 5 H.P.

(d) Flow-meter:

A rotameter was attached to the pump in order to measure the flow rate of the water. It was a variable area flow meter with a capacity of 10 gpm.

(e) Pressure Probes:

The pressure is measured at four places in the loop. Bourden gauges were placed at lower and upper plenums respectively to measure the systems pressure. The pressure transients were measured at two places, one inside the lower plenum and another just at the throat of the break. These were measured by two strain gauge type pressure transducers, manufactured by Consolidated Electrodynamics, Pasadena, California (a subsidy of Bell and Howell). The specifications were as follows:

Number	Range	Max. Temp.	Nat. Freq.
CEC 4-313-0001	0-300 psia	250°F	15 KHz
4-312-0001	C-150 psia	250°F	15 KHz

The transducers did not have connectors and cables. The connectors were fabricated on a printed circuit, in the printed circuit lab. and precision shop.

f) Quick Acting Valves:

One of the important work done during this thesis was to make quick acting valve. This valve was needed to

simulate the rupture in a pipe. In general the rupture time in a nuclear power plant is less than a millisecond and it is difficult to have such a fast valve. In addition to these aspects, the valve must operate at high temperature ($\approx 100^{\circ}C$) and high pressure ($\approx$ 200 psi). Initially, we started with a spring device, but it had many limitations, like the high time constant ($\approx$ 50 ms). Moreover, it was a difficult task to make it leak proof. Curet and Shiba[24] used two discs, which were ruptured by pressurizing the air between them. This device has many disadvantages. For example, the pressure which is induced externally gives a shock wave which travels opposite to the internal pressure shock wave. Secondly a big compressor will be required which will create many more problems. Accordingly we tried few different approaches for creating the rupture. In one, the rupture disc. was burnt by an electric arc. A high voltage was applied between rupture disc (aluminium foil) and an electrode, by an induction coil (15 KV). But it did not work, as the aluminium foil took a long time to burn and secondly it could not sustain high pressure. Another attempt was made to construct a device which could be controlled by a magnetic field. It was found, however, that a very high magnetic field would be needed to operate this device and the idea was accordingly given up.

Finally, we tried plastic discs, which could sustain high pressures and temperature. Ordinary plastic sheet was used, but at high temperature it melted down and the attempts to insulate them thermally went in vain. This led us to use **mylar** sheets (a type of plastic which can sustain high temperature ($\sim 150^{\circ}C$) and pressure (~ 200 psi)). Two **mylar** sheets with a resistance wire in the form of a coil of a fixed diameter sandwiched in between formed the rupture disc. The resistance wire was heated externally and it took less than 1 μ sec.to rupture the disc. Thus a new technique was developed to simulate rupture in a pipe.

3.3 Instrumentation:

The instrumentation was designed to measure the following variables.

(a) Stagnation pressure

(b) Pressure transients

(c) Temperatures, and

(d) Flow rates

(a) Stagnation Pressure:

The stagnation pressure i.e. the pressure inside the loop was measured at two places, (one near lower plenum, another near upper plenum) by standard Bourden gauges, which wer available in our laboratory.

(b) Pressure Transients:

The pressure transients were measured for both static and stagnation pressure, as soon as the rupture occured. These transients were measured by two pressure transducers. The static pressure was measured just at the throat of the break and stagnation pressure transients were measured at the lower plenum. The static pressure was recorded on a storage oscilloscope (ECIL, 768 S). The stagnation pressure transients were recorded on a multi-channel recorder (Encardio-rite, 4 channel strip chart recorder). The oscilloscope can not take differential input, so one differential amplifier was designed[25], for the pressure signal. The manuals for these transducers were supplied by Bell and Howell.

(c) Temperatures:

There were seven locations to measure the fluid temperature. Five places were in test section and one each for lower and upper plenum. Copper constantan thermocouples were used. The temperature was recorded by three recorders at three locations. Other locations were set idle as we did not have multichannel temperature recorder. At one location, i.e. at lower plenum, the temperature was measured by a milli-volt potentiometer, which could read variations of 0.1°C. At the second location i.e. middle of test section

it was recorded by a single channel temp. recorder (Leeds and Northrup,1channel Time constant 2 minutes). At third location, i.e. test section, it was recorded on encardio-rite multi-channel recorder.

(d) Flow-rates:

Two flow-rates were measured. First one, is the pumping flow rate i.e. the amount of water flowing through the loop. This was measured by variable area flow meter.

The other more crucial flow rate is the, critical mass flow rate. One indirect method, which was used by Shiba and Curet[24], is to measure the amount of water collected in certain amount of time, but it has one drawback, i.e. they assumed flow rate to be constant throughout the process of flow down, In fact flow rate does change and we developed a method to measure flow-rate continuously as the pressure varies. An attempt was made by using strain gauge, but it did not work, as the vibrations due to pressure and burst were too much and we got spurious signals. The other technique is to use load cell, which can record change in amount of water, but the water tank cannot be mounted on the top base ($\frac{1}{2}$ in.dia) of the load cell. The technique employed in our work was simple but very effective. The principle is to measure resistance of water as it changes. This was done by putting two copper wires inside a glass tube, which is attached to the

water tank as shown in Figure 3.3 . A constant current (d.c.) supply was given to the copper wires. The voltages across the wires changes with the water level. This change in voltage was recorded on the storage oscilloscope (ECIL, 768 S) The output was of the order of 0.5 volt/mm change in water level (current 20 mA). The constant current supply was designed and fabricated in our laboratory[26].

3.4 Experimental Procedure:

Initially, cold water was filled in the thermally insulated loop. The water is circulated in the loop in equilibrium with the help of 4 stage pump. The heater was switched on and the water was circulated, till the temperature went upto 100^{o}C. The oscilloscope, Encardio-rite recorder temperature recorder and the various supplies were switched on. The temperature at lower plenum was measured with the help of milli-ohm-potentiometer continuously. As the temperature reached 100^{o}C the mylar discs were burnt with the help of a high current(50 Amp). The oscilloscope was kept in a single sweep mode and the signals triggered it automatically. The static pressure transients and water level change, were recorded simultaneously on storage oscilloscope. The temperature signal near the lower plenum and stagnation pressure transient were recorded on encardio-rite strip chart recorder.

CHAPTER IV

RESULTS AND DISCUSSION

4.1 Results and Conclusions:

As mentioned in Chapter III, the values of the stagnation and static pressures, and water level were measured for four different pressures. The temperature transient was measured only for 100 psi. Since no significant transients were observed the temperature measurement was discontinued for other pressures. The pressure transients (static and stagnation) are shown in Fig. 4.1 and 4.2. The lack of temperature transients arise mainly due to the low heat fluxes involved. Table 1 summarises the readings obtained together with the values calculated from Moody's and Fauske's models.

Table 1: Calculated and Measured Values of G_c

Set No.	Stagnation Pressure Kg/cm^2	Static pressure Kg/cm^2	Measured G_c Kg/m^2-sec	Calculated G_c	
				Moody's Model Kg/m^2-sec	Fauske's Model Kg/m^2-sec
1	3.52	1.07	14702	8480	8697
2	4.40	1.09	14260	3885	3035
3	6.10	1.10	13240	3388	1614
4	7.80	1.27	12220	2661	1271

The first calculation (Moody's) requires stagnation properties for the steam-water mixture and also the specific enthalpies for the pure gas and liquid at the static pressure. Since, in our case the inlet mixture contained no steam, the stagnation enthalpy was that of the liquid alone. The property

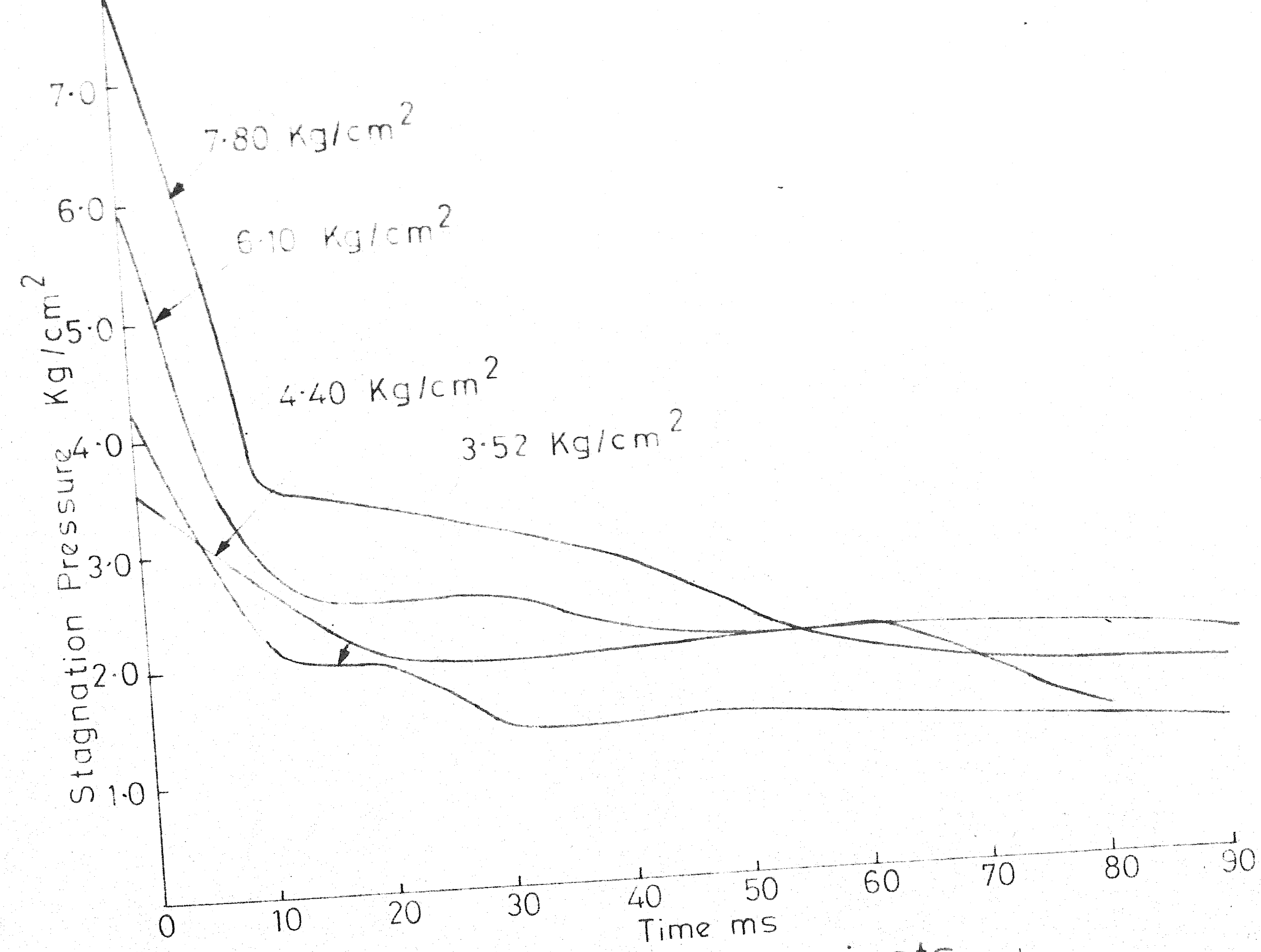


Fig.4·1 Stagnation Pressure Transients

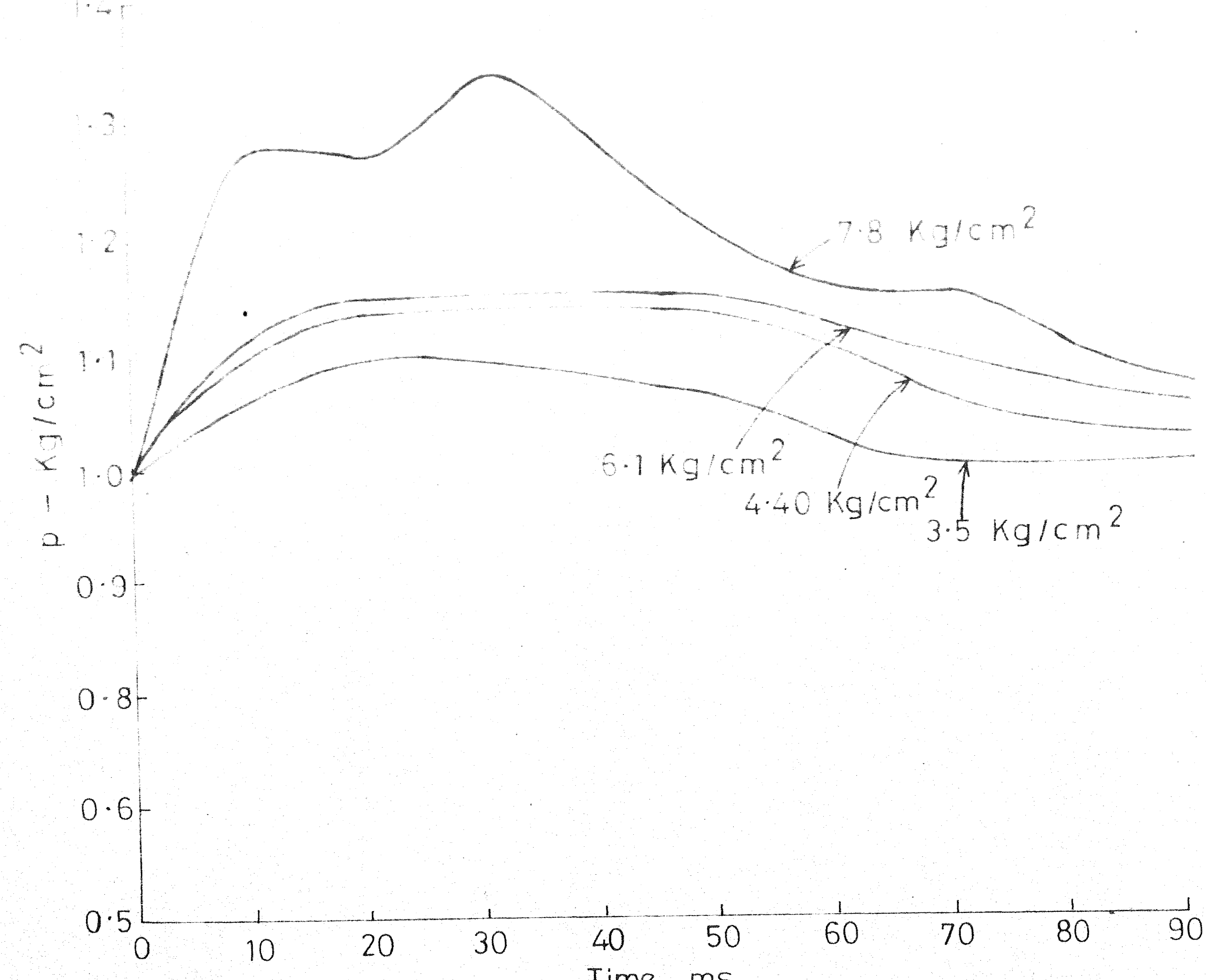


Fig.4·2 Static Pressure Transients

values were taken from the ISI steam tables[28]. The calculation of G_c by Fauske's model was somewhat more tedious since various differentials with respect to pressure had to be calculated. This was done by the standard method of dividing ordinate differences by abcissa differences ($\Delta s_g/\Delta P$). The values, again, being obtained from the steam tables.

From the results above it seems that both Moody's and Fauske's models predict values which are significantly different from the experimental ones. It is difficult to say in the present case which, if any, of the two models apply.

In our view the following are probably the major factors which led to the above discrepancies and which need to be rectified in future experimental work on the present set-up:

1. The main source of error seems to lie in the measured values of flow rate. The rather short time spans involved (~ 50 ms) made it necessary to measure the level change rather than the averaged flow rate that has usually been measured by other workers in the field. The measurement was affected by the impact of the ejected fluid and the recorded level change was significantly more than the actual change in level. This was also the reason why pressures higher than 100 psi were not considered. An accurate

experimental arrangement would probably require baffles to break up the impacting fluid, ideally a very fast (open-shut) quick acting valve with variable 'open' times would solve the problem.

Fauske's model uses the experimentally measured flow rates to calculate quality and the discrepancy between the two calculated values at the higher pressures (6.1 and 7.3 Kg/cm^2) can probably be traced to high values of the measured flow rate.

2. At the inlet (stagnation point) the two phase mixture contained only liquid since a boiler was not available. This led to very low qualities after the break (static point).

3. The low heat fluxes obtained from the heater prevented the system from reaching high temperatures. This again contributed to low quality.

4. The total water in the system was quite small (7-8 litre) Thus the period for which critical flow actually existed was quite short, of the order of 10-20 ms. This was a big handicap since the fastest recorder available had a minimum time base of 10 ms. Both static pressure and temperature measurements suffered from this drawback.

5. The short length of the break meant that the assumption of equilibrium between phases was seriously in error. Both the Fauske and Moody calculation have been vitiated by this shortcoming.

6. Fauske's and Moody's models are not applicable to low quality flows. As mentioned earlier the output quality was low, the maximum being 2.87 and it is doubtful whether the models actually apply, however in the absence of any other these two had to be used for purposes of comparison.

4.2 Scope for Future Work:

(a) Experimental:

One of the major problems in all LOCA simulations has been the extreme combination of conditions involved. For example the high heat flux in nuclear reactors is quite difficult to get in a laboratory environment. Besides, critical flows in simulations exist for a very short time, of the order of milliseconds which makes sophisticated instrumentation necessary.

No existing experimental assembly actually simulates a LOCA in a real nuclear reactor. So a nuclear reactor meant specifically for LOCA experiments is being constructed in the United States.

While an attempt has been made in the present set-up to achieve reactor conditions, several improvements need to be made. These can be summarised as follows:

(i) To match heat fluxes an induction heater can replace th resistance wire heater being used at present.

(ii) A heat exchanger can be provided in the loop to simulate the primary heat exchanger in the reactor.

(iii) Dimensionless parameters like Reynolds and Prandtl numbers can be made equal to those in reactors.

(iv) A surge tank can be provided to alter systems pressure without changing flow rates.

(v) A multichannel recorder which can record fast transients can be attached to the system. Better still, the signals can be recorded on a real time data acquisition system like the IBM 1800. This will give a detailed picture of the transients.

(b) <u>Theoretical</u>:

The two major present equilibrium models use either energy (Moody) or the momentum (Fauske) equation along with the continuity equation to maximise the two phase flow rate. A general approach using the continuity, momentum and energy equation with the three independent variables, pressure, slip ratio and quality would probably give a better model.

The equilibrium models, however, are clearly inadequate. In no sense can the two phases be said to be in equilibrium after the break. This means that non-equilibrium models[29] have to be developed and experimental results compared with them.

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